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Development of an engineering methodology for thermal analysis of protected structural members in fire

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ABSTRACT: In order to overcome the limitations of existing methodologies for thermal analysis of protected structural members in fire, a novel CFD-based methodology has been developed. This is a generalised quasi-3D approach with computation of a "steel temperature field" parameter in each computational cell. The methodology accommodates both uncertainties in the input parameters and possible variants to the specification by means of parallel calculations. A framework for the inclusion of temperature/time-dependent thermal properties, including the effects of moisture and intumescence, has been established. The method has now been implemented as the GeniSTELA submodel within SOFIE RANS CFD code, with initial validation against results from full-scale fire tests. Model sensitivities have been demonstrated revealing the expected strong dependencies on certain properties of thermal protection materials. The code is verified as a generalised thermal analysis tool, with potential to provide a much more flexible means of assessing the thermal response of structure to fire than has been available hitherto.

1 INTRODUCTION

Increasing interest in assessing the performance of structures in fire is driving the development of an array of modelling methodologies to be used in fire safety engineering design. Whilst traditionally most code-based design has been based on simple calculations, referencing measured fire performance in standard tests, the progressive shift towards performance-based design has opened the door to use of advanced methods based on numerical models. These approaches will not replace standard testing, but they can already be used in a complementary fashion, to extend the application of test data.

Some simplified modelling methods have also been established, such as the protected member equation in Eurocode 3 (EC3) (BSI 2002), but as with all semi-empirical methods the results will tend to be conservative and there are of necessity a number of simplifying assumptions. CFD-based methodologies can in principle provide a much more detailed description of the thermal environment and the effects of localised heating, which could be used in conjunction with thermal analysis models to examine structural performance. In previous work, a dedicated fine-mesh thermal modelling tool, known as STELA (Solid ThErmaL Analysis), has been implemented with the RANS CFD code SOFIE (Kumar et al. 2005). However, this research suggests that detailed thermal analysis of structural members in the context of simulations of full-scale building fires remains problematic. This is partly due to the difference of scale between the mesh which can be afforded for the fire and that required for the thermal analysis of the structure, a particular problem with structured meshes, and also the generally high computational demands for coupled analyses. Moreover, all existing approaches are limited to a specific structural arrangement of interest since it is necessary to define its specification in advance. Simulations must be repeated if details such as the structural geometry or the thermal property are changed, a very inefficient procedure.

A more general and flexible methodology has now been proposed, still within the context of a CFD fire simulation, as reported previously (Liang & Welch 2006). This is based on computation of a set of "steel temperature field" parameters within the whole of the calculation domain, accommodating, by means of parallel calculations, both uncertainties in the input parameters and possible variants to the specification. Hence the need for repeat simulations is bypassed. This new generalised methodology is called GeniSTELA (Generalised Solid ThErmaL Analysis) and also implemented in SOFIE.

This paper addresses the further development of this methodology and its extended application. In particular, the implementation of modelling representations for the effects of intumescent performance in fire is described.

2 METHODOLOGY DEVELOPMENT

2.1 *Brief description*

When protected steelwork is exposed to fire, heat is transferred to the structure through a layer of insulation. The transient heating response of the member can in principle be described using conventional methods based on numerical heat transfer. However, full 3D analyses impose great computational demands, due to the large numbers of cells required in order to adequately resolve the steep thermal gradients during the initial heating Even if the computational resource is available, in simple deterministic models there is no direct mechanism to accommodate uncertainties in the thermal properties and member specification. To overcome these problems, with an appropriate balance between accuracy and tractability, a novel quasi-3D analysis methodology has been developed (Liang & Welch 2006). This is achieved by constructing a generalised 1D model and further considering the 2D or 3D effects within the heat transfer processes by appropriate approximations and corrections. The computations are performed in each gas-phase CFD cell in the computational domain.

2.2 *Generalised 1D model*

The generalised 1D model is constructed through analysing the heat transfer to and within an element in an idealised protected steel member assumed to be exposed to heat on two faces, as shown in Figure 1 below:

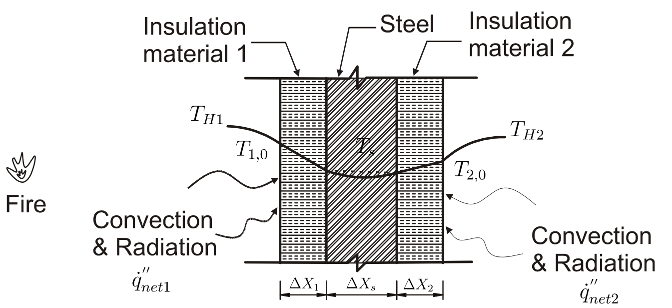

Figure 1. Schematic of heat transfer to protected steel member

This element is supposed to be representative of a slice of a protected steel structure, e.g. a finite section of a flange or a web; two faces are used to allow for situations where the exposure conditions on each side might vary, encompassing also the case of hollow sections with very different exposures on the inside of the structure, though in that case the insulation thickness on the inside is reduced to zero.

The generalised 1D model provides a modelling framework which exploits a simple thermal penetration model for the protection coupled to an essentially lumped parameter representation of the steel heating. The governing equations for this model are derived by considering the net energy balance together with surface heat transfer boundary conditions (Carslaw & Jaeger 1959), as given below:

Energy balance equation:

$$\frac{\partial E_{system}}{\partial t} = \dot{q}''_{net} \tag{1}$$

i.e.

$$\rho_s \cdot c_{ps} \cdot \frac{\partial T_s}{\partial t} \cdot \Delta x_s + w_{p1} \cdot \rho_1 \cdot c_{p1} \cdot \frac{\partial T_1}{\partial t} \cdot \Delta x_1 + w_{p2} \cdot \rho_2 \cdot c_{p2} \cdot \frac{\partial T_2}{\partial t} \cdot \Delta x_2$$
$$= h_{c1} \times (T_{H1}^{(n)} - T_{1,0}^{(n)}) + \dot{q}''_{r1} - \varepsilon_{m1} \cdot \sigma \cdot T_{1,0}^{(n)4} +$$
$$h_{c2} \times ((T_{H2}^{(n)} - T_{2,0}^{(n)}) + \dot{q}''_{r2} - \varepsilon_{m2} \cdot \sigma \cdot T_{2,0}^{(n)4}$$

The terms shown in the expanded equation here represent, respectively, the transient heating of the steel and protection layer on each side, and the convection, radiation and reradiation for each surface of the protected member. A semi-empirical treatment is adopted for transient heating, allowing for spatially- and temporally-varying temperature gradients within the solid. The boundary conditions are supplied from the heat transfer solution for the surfaces, using the following equations:

$$\dot{q}''_{net1} = \frac{k_1}{w_{p1} \Delta x_1} \cdot (T_{1,0}^{(n)} - T_s) \tag{2}$$

i.e. $h_{c1} \times (T_{H1}^{(n)} - T_{1,0}^{(n)}) + \dot{q}''_{r1} - \varepsilon_{m1} \cdot \sigma \cdot T_{1,0}^{(n)4} = \frac{k_1}{w_{p1} \Delta x_1} \cdot (T_{1,0}^{(n)} - T_s)$

$$\dot{q}''_{net2} = \frac{k_2}{w_{p2} \Delta x_2} \cdot (T_{2,0}^{(n)} - T_s) \tag{3}$$

i.e. $h_{c2} \times (T_{H2}^{(n)} - T_{2,0}^{(n)}) + \dot{q}''_{r2} - \varepsilon_{m2} \cdot \sigma \cdot T_{2,0}^{(n)4} = \frac{k_2}{w_{p2} \Delta x_2} \cdot (T_{2,0}^{(n)} - T_s)$

where:
σ is Stefan-Boltzmann constant ($5.67\text{x}10^{-8}$ W/m^2/K^4);
$\dot{q}''_{r1}, \dot{q}''_{r2}$ are incident heat fluxes on each side;
$T_{1,0}^{(n)}, T_{2,0}^{(n)}$ are surface temperatures at gas/solid interfaces;
T_s, T_1, T_2 are steel and average protection layer temperatures, respectively;
h_{c1}, h_{c2} are convection coefficients;
ε_{m1}, ε_{m2} are emissivities of protection layers;
ρ_s, ρ_1, ρ_2 are densities of steel and protection layers, respectively;
Δx_s, Δx_1, Δx_2 are thicknesses of steel and protection layers, respectively;

w_{p1}, w_{p2} are weight factors of protection layers, defined in terms of the thermal penetration depth of the protection, as given more precisely in Equation 4

$$w_p = \min\{\frac{A_{actual}}{A_{model}} = \frac{\delta}{\Delta x_p}, 1\} \tag{4}$$

where

$$\delta = 2 \cdot \left(\frac{k_p \cdot t}{c_p \cdot \rho} \right)^{1/2}$$

c_s, c_{p1}, c_{p2} are specific heat of steel and protection layers, respectively;
k_1, k_2 are thermal conductivity parameters of protection layers.

The temperature/time dependent characteristics, including moisture and intumescence effects, are incorporated in certain parameters for generalisation of the methodology. The treatment for moisture was described in the previous paper (Liang & Welch 2006) whilst intumescence is described in section 3 below.

It is well-known that the above situation is a strongly coupled problem, with the net heat fluxes at the gas-solid interface very much dependent on the surface temperature, but both also related to the transient thermal response of the structure itself. Numerical instabilities might become evident if inadequate solution procedures are used; these are overcome using a Newton-Raphson method to update the surface temperature from the heat transfer boundary condition governing equations and thereafter, with the updated surface temperature as a boundary condition, solving the overall energy balance equation (Equ. 1) with the Runge-Kutta method to obtain the steel temperature. Further details of the solution procedure are provided in the earlier paper (Liang & Welch 2006).

2.3 *Quasi-3D model*

Use of a fundamentally 1D treatment is essential, considering the costs of doing a full 3D analysis in every computational cell and including a sufficient number of parametric variations. However, adoption of a simple 1D model for thermal analysis could clearly lead to some modelling inaccuracies. These could in principle be in either direction, resulting in either conservative (over-design) or non-conservative (unsafe) results. The former aspect is not a major concern since the method is in any case far more flexible than other simple models, and by using generalised treatments conservatism is already greatly reduced. The latter aspect is a more obvious problem, and in order to overcome it methods for treating important 2D and 3D effects are needed. A number of corrections factors have been implemented in the model, encompassing the factors indicated in Figure 2, i.e. the junction effect, end effect, heat sink effect and axial temperature gradient effect, as described in the earlier paper (Liang & Welch 2006). It is important to note that these effects are only critical where they negatively impact the performance of the member, i.e. increase the solid temperatures, and in the majority of cases the opposite is true, i.e. the default procedure is a good representation of the "worst" case. Thus, while it is vital to show that these possible corrections have been appropriately considered, their effect on the final results has been found to be fairly limited.

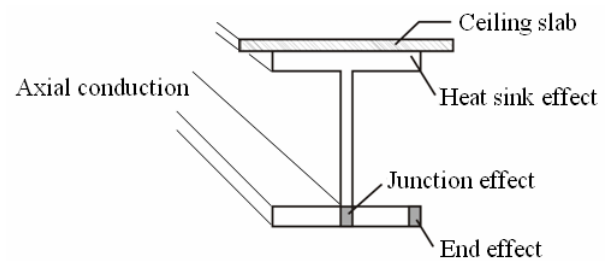

Figure 2. Cross-section of the beam with locations of possible correction effects

3 MATERIAL PROPERTIES

The aforementioned model might be considered as a reasonable representation of the *fundamental* aspects of the heat transfer phenomena. However, in practice several factors are found to have a great impact on the transient response, in particular the thermal properties of the protection materials, which affect the surface temperature and thus the steel temperature. It is known that their thermal properties are often strongly temperature/time dependant and the use of constant values may result in significant errors in some cases. The methodology developed here aims at generalising the thermal analysis to accommodate all important phenomena; conventional approaches to treatment of moisture effects have already been implemented, referencing modified specific heats and thermal conductivities (Liang & Welch 2006). This is now extended further to include the effects of intumescence, clearly of great practical relevance to the case of protected steelwork. In order to do so, geometrical and density variations must also be explicitly treated.

Intumescent materials are an increasingly popular form of fire protection, due to a number of advantages arising from the fact that they can be applied as thin, aesthetically pleasing, coatings either before or after construction (Goode 2004, Jimenez et al. 2006, Bailey 2006a). When in contact with high temperatures, they will swell and form a layer of carbonaceous char which has much greater thickness than the initial state. The char subsequently acts as a thermal barrier to effectively protect the substrate against increase in temperature. Nevertheless, during the process of intumescence, the material properties are severely changed along with mass transport and endo- and exothermic reactions. These properties include thermal conductivity, specific heat, density and thickness of the intumescent layer.

Several research studies have been carried out to determine the effective intumescent thermal properties by experimental tests, in conjunction with some form of numerical analysis. These include bench-scale cone calorimeter tests and small-scale furnace tests on coated plates (Bartholmai et al. 2003, Bartholmai & Schartel 2007), and furnace tests on cellular beams (Bailey 2006b). The first authors conducted studies on typical water-based and solvent-containing intumescent systems (Bartholmai et al 2003) and later on a high-performance material, i.e., epoxy resin containing boric acid and phosphate-based flame retardant (Bartholmai & Schartel 2007). The results from the former showed a significant slow down of temperature increase between 200-300°C, due to intumscence, i.e. the formation of an insulating char and other coacting energy absorbing processes; temperature influences during the latter tests also resolved a damping effect at 150°C due to the endothermic reaction of boric acid, which also releases water. Layer thickness effects were non-linear. Considering first the geometrical expansion, a simple conceptual model would suggest that thermal equivalence to a finite thickness problem can be achieved by simply scaling the thermal conductivity by the layer thickness, *d*, giving an effective thermal conductivity, *k/d*. Density is scaled in the same way, and specific heat by the inverse of *d*, but these parameters always appear as factors so these scalings vanish in the term ρc_p.

The description of the temperature-dependent intumescent thickness, d, can be determined from an expression for the expansion ratio. We postulate that this will fit the general form:

$R = 1$	$T < T_{lower}$	(5)
$R = 1 + \frac{1}{2}\left(R_f - 1\right)\left(\frac{T - T_{lower}}{T_{mid} - T_{lower}}\right)^n$	$T_{lower} \leq T < T_{mid}$	(6)
$R = R_f - \frac{1}{2}\left(R_f - 1\right)\left(\frac{T_{upper} - T}{T_{upper} - T_{mid}}\right)^n$	$T_{mid} \leq T < T_{upper}$	(7)
$R = R_f$	$T \geq T_{upper}$	(8)

Where,

R is the time-dependent expansion factor;
R_f is the final expansion factor;
T_{lower}, T_{upper}, T_{mid} are the critical temperatures where scaling factor changes;
T is the current intumescent temperature;
n is a shape factor power
Here, besides the relevant temperatures limits, the critical controlling parameters are the shape factor n and the overall expansion ratio R_f. An approximate calibration has been performed by comparison with test data, including the results of Bartholmai (Bartholmai & Schartel 2007), giving a value of n=2. Taking an approximate temperature range from the DTG results of the latter study, and assuming R_f=10, gives the following curve:

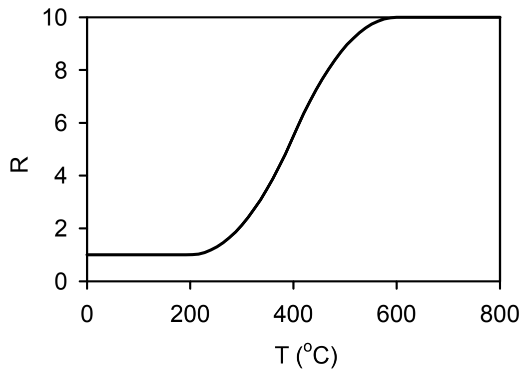

Figure 3. Scaling factor R change with temperature

In fact, a variety of overall 1D expansion ratios have been reported in the literature (10 - Desanghere & Joyeux 2005; 15-30 – Goode 2004; 50-220 – Bartholmai 2003).

The key parameter for the thermal model is the conductivity, or its scaled value, i.e. *k/d.* the conductivity itself is affected by fundamental changes in the material as it intumesces. This has been reported by Tan (Tan et al. 2004) and Bartholmai (Bartholmai et al 2003, Bartholmai & Schartel 2007). Unfortunately, the effect is non-linear and very dependent on initial thickness, and most pronounced at the smaller thicknesses typical of real applications; hence, there would appear to be no substitute for its direct experimental determination. Work is currently underway at Edinburgh to determine thermal properties of intumescents in cone calorimeter tests. In the meantime, the various literature results would suggest an initial increase followed by a fall during intumescence and finally a sharp rise during material degradation. For our initial model, we have fitted indicative values from Bartholmai (Bartholmai & Schartel 2007), as shown in Figure 4 below:

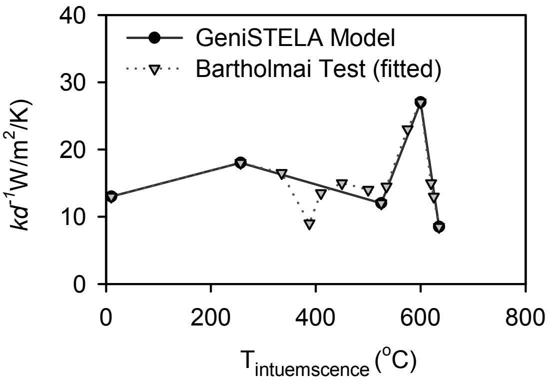

Figure 4. Comparison of thermal conductivity between generalised model and Bartholmai test (Bartholmai & Schartel 2007)

4 MODEL IMPLEMENTATION AND VERIFICATION

The above conceptual model has been implemented as a submodel called GeniSTELA within SOFIE RANS CFD code (Lewis et al. 1997). Representative empirical values are adopted for some terms such as the initial conditions, the dry thermal properties, moisture content, etc., and their influence has been studied by exercising the model with different sets of input parameter values. The performance of the model was assessed by performing sensitivity studies, looking at the effects of a range of numerical and physical parameters. Comparisons were also made with the results from the EC3 protected member equation (BSI 2002).

The case used for verification studies is the protected steel indicative, UC254x254/73, in the full-scale tests on a 12m x 12m compartment undertaken at BRE Cardington (Welch et al. 2007); this member was protected with about 25mm of Fendolite MII sprayed fibre (base ρ=680kg/m^3, k=0.19W/m/K). Figure 6 shows the test compartment while Figure 7 shows the computer simulation.

In the test a variety of thermal parameter measurements were made, encompassing conditions in the gas phase (temperatures, velocities and heat fluxes) and in the solid phase (steel temperatures in protected beams, columns and indicatives, with and without protection); this study also serves for an initial validation of the model, comparing the model predictions with the measured steel temperatures in the protected indicative.

Figure 5. BRE 12x12m large compartment fire test

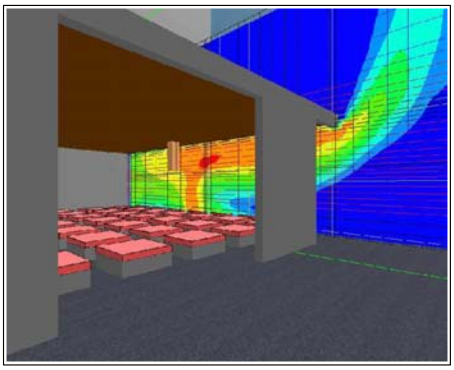

Figure 6. BRE 12x12m large compartment fire modelling

5 RESULTS

5.1 *Simulation results*

Gas and steel temperatures were computed using SOFIE and the coupled GeniSTELA code for the BRE 12x12m large compartment fire test. In qualitative terms the results showed the expected differences in steel and gas temperature fields, with relatively higher steel temperatures within the depth of the compartment compared to at the openings. This is consistent with the fact that the thermal exposures are more severe in fully engulfed regions (Welch et al. 2007), and the model predictions from GeniSTELA are heavily influenced by the radiative terms, $\dot{q}_r''$, derived directly from the CFD calculation.

Figure 7 shows the temperature predictions for the protected indicative within the compartment. There is a large temperature gradient across the protection.

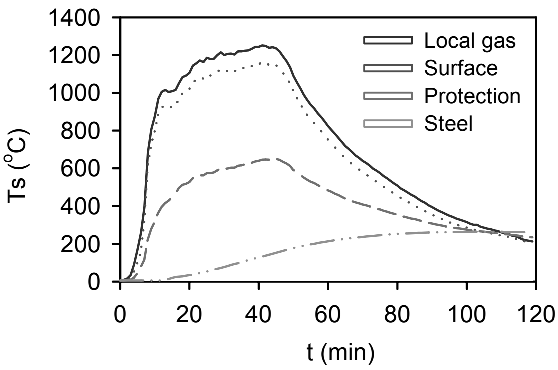

Figure 7. Temperatures at protected indicative, test 8

Figure 8 shows a comparison of the predictions of steel temperature with the test together with EC3 prediction. The latter exceeds the measure temperature leading to a slightly conservative result, while the prediction from GeniSTELA indicates a sufficient match with the test within the predicted time.

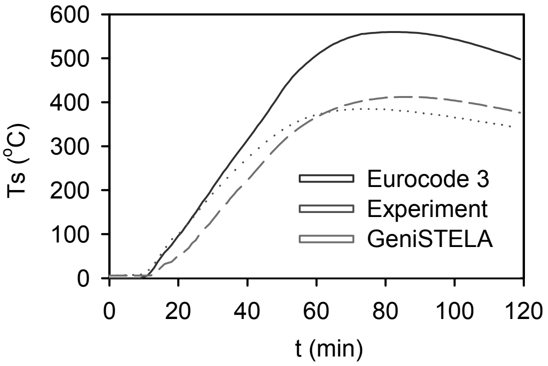

Figure 8. Comparison of steel temperatures by different methodologies

By default, GeniSTELA is called after every radiation call, i.e. every 10 flowfield iterations; it was confirmed that computational demands for a single instance of the method were relatively low, less than 1%, and full parallel call of the solver for a large parametric study is therefore feasible.

5.2 *Sensitivity study results*

Some results from the sensitivity study are shown in Figures 9-10 for the effects of changing the steel flange thickness (spanning UC 254x254/73,107,167) and the protection thickness (12.5 to 50mm). The results for changing the protection thermal conductivity mirror the latter, and show the expected strong influence of protection properties.

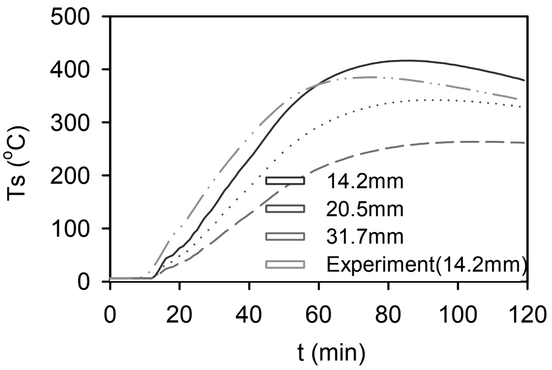

Figure 9. Effect of flange thickness on steel temperature

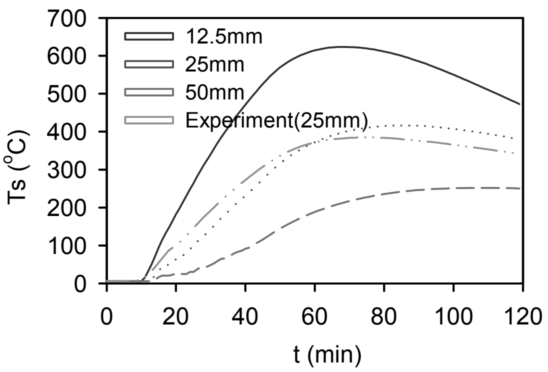

Figure 10. Effect of protection thicknesses on steel temperature

6 CONCLUSIONS

A generalised methodology for thermal analysis of protected steel structures in fire is described. A framework for the inclusion of treatments for intumescence effects has now been established, with provision for simple calibration in the case of each specific formulation of interest. The GeniSTELA implementation of the method is based on parallel computations spanning the range of cases of interest, providing a generalised methodology. The initial results confirm the sufficiency of the algorithms adopted and comparisons with measurements in a post-flashover compartment fire test are satisfactory. Computational demands are found to be acceptable. Strong dependencies on the thermal properties of the protection materials are observed in the sensitivity studies. These results serve to illustrate the importance of using generalised methodologies in tackling thermal response problems.

7 ACKNOWLEDGEMENTS

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